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CONTENTS

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PROBLEM STATUS

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AUTHORIZATION

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J-INTEGRAL CHARACTERIZATION OF IRRADIATED STAINLESS STEELS

INTRODUCTION

Austenitic stainless steels (SS) are employed as structural materials for the current generation of liquid metal fast breeder reactors (LMFBR). These steels normally exhibit ductile or fully plastic behavior in the annealed condition. However, the initial properties can be degraded due to mechanical working (cold work) and other environmental conditions inherent to reactor operation, e.g., irradiation, high-temperature-induced creep, and incompatibility with the liquid metal reactor coolant. These conditions, alone or in combination, can lead to embrittlement of the steel. An example of this type of toughness degradation is provided by the failure of a 304 SS control rod thimble taken from the EBR-II reactor (1). The highly irradiated (1.7×10^{23} n/cm^2 > 0.1 MeV) thimble fractured as a result of an inadvertent bending during a hot-cell machining operation, and several pieces of metal were expelled.

In the event of toughness degradation due to the processes mentioned above, the possibility of fracture becomes a necessary consideration. Consequently, the toughness of the material must be characterized in terms of a critical flaw size vs stress level relationship that can be (a) integrated into the structural design relationships, and (b) used to provide fracture-safe assurance during reactor operation.

An analytical description of the critical flaw size vs stress level interaction has been highly developed for materials whose toughness falls into the linear elastic or brittle regime, i.e., linear elastic fracture mechanics (LEFM). However, it is expected that the toughness of irradiated SS will not be so severely degraded such that LEFM techniques are applicable. Instead it is anticipated that elastic plastic conditions will prevail. Unfortunately, an analytical description of the fracture behavior for this regime, similar to that available for the linear elastic regime, is extremely complex. This stems from the large degree of plasticity in the vicinity of the flaw which causes the metal to exhibit a nonlinear behavior. Several advanced procedures are being considered to analyze the elastic plastic regime. Of these the J integral appears to offer the most attractive possibilities for a near-term engineering solution in this area.

An introductory description of the J integral, including approximation techniques for its evaluation, is presented here. This procedure has been applied to evolve the first assessments of irradiated 304 SS in the cold-worked (CW) and annealed conditions. Finally, the practical difficulties relating to a meaningful interpretation of the observed behavior are highlighted as areas where current research efforts should be directed.

DEFINITION OF THE J INTEGRAL

It is anticipated that the J integral formulated by Rice (2,3) can be used to define a material toughness property or failure criterion for use in predicting a critical flaw size vs stress level relationship for a structure in the same sense in which K_{Ic} is used in LEFM. Contrary to the requirement of elastic behavior imposed on K_{Ic}, the J integral may be used in the elastic plastic and fully plastic regimes in addition to the linear elastic regime. This concept has a firm anchor in the linear elastic regime, and the critical J, or J_c, reduces to $\mathscr{G}_c$ in the limiting case of brittle materials. Like LEFM, the J integral provides a means for defining fracture initiation. The method is presently developed only for two dimensions (that is, plane strain or plane stress), and J refers to the crack opening mode or mode I. Currently the J integral is considered a mathematical concept, and research programs are in progress to evaluate its usefulness for application to real materials.

The J-integral method addresses the elastic plastic problem from a fresh viewpoint. It is known that the chief source of difficulty in an elastic plastic analysis lies in describing the material behavior in the crack tip vicinity. First, the J integral provides a means of characterizing this area without focusing attention directly at the crack tip. This is accomplished by expressing the average crack-tip elastic plastic field in terms of deformation energy. Second, the J integral can be evaluated in a straightforward manner from any of the numerous fracture specimens in current use. The scenario for the practical application of J is as follows.

- Determine the critical J value for fracture initiation in a laboratory specimen.

- Compute the value of applied J in an actual structure where the material properties and flaw sizes are known. The computation is simplified in that the complex analysis of the material behavior at the crack tip is circumvented.

- Compare the calculated value of applied J with the measured critical J determined from the laboratory specimen. The degree of safety from fracture initiation is then based on the fact that J_c is a material property, albeit yet to be demonstrated. Thus instability is predicted when the applied J equals J_c.

In general terms, J is based on the energy associated with the deformation within an arbitrary two-dimensional region of uniform thickness surrounding the crack tip. It is important to note that this area is truly arbitrary and thereby can be chosen to avoid the difficult problem of modeling the crack tip behavior. Indeed, when J is determined experimentally, this area is extended to the specimen boundaries. The average deformations within this area are expressed in terms of potential energy PE per unit thickness, i.e., units of in.lb/in. Specifically, the J integral is the negative rate of change of this potential energy with respect to crack extension. In equational form,

$$J = -\frac{dPE}{da}, \tag{1}$$

where a is the crack length and J has the units of in.lb/in.^{2}. For a brittle material this quantity can be physically interpreted as the energy that is available for further crack extension; it is the crack driving force $\mathscr{G}$ that is well known from LEFM theory.

Unfortunately, this physical interpretation of J cannot be similarly related to crack extension for elastic plastic materials. This stems from the added requirement to consider plastic deformations for these materials and the fact that these deformations are not reversible.* The requirement of reversibility as applied to crack extension when interpreting

*It must be remembered that reversibility refers to elastic behavior and should not be confused with nonlinearity. In fact, deformation processes in nonlinear elastic materials are considered reversible.

the energy associated with the J integral is expressed in the following manner. For reversibility to apply, one must demonstrate that the same result is obtained both by (a) loading a cracked body and then permitting an incremental crack extension, or (b) extending the crack and then loading the body. This equality cannot be shown for plastic (nonlinear) deformations. Consequently, the J integral cannot be directly related to crack extension in the sense of a crack driving force $\mathcal{G}$, as is the case for linear elastic materials. The physical significance of J for elastic plastic materials must therefore be considered as that of an energy comparison of two similar bodies, loaded in the same manner and having incrementally differing crack sizes. J can also be considered as a measure of the shape of the elastic plastic field at the crack tip. The object of current research is to show to what extent this method of assessment of the elastic plastic field is a property of the material.

Specifically, the J integral is defined by the equation

$$J = \int_\Gamma \left[W dy - \mathbf{T} \cdot \frac{\partial \mathbf{u}}{\partial x} dS \right], \tag{2}$$

where W is the strain energy density (in.lb/in.^{3}), $\mathbf{T}$ is a surface traction (lb/in.^{2}), and $\mathbf{u}$ is a deflection (in.) (Ref. 3). This equation refers to the potential energy difference between two similar bodies having identical cracks that differ only in the incremental sense (Fig. 1). The path of integration Γ is around the notch tip and defines the surface of boundary S over which the J integral is calculated.

EVALUATION OF J (COMPLIANCE METHOD)

Since J is expressed in terms of PE (Eq. (1)), it is necessary to measure only this quantity to evaluate J experimentally. The experimental curve of PE as a function of crack length a for identical specimens of varying crack length can then be differentiated to get dPE/da, and hence J. The compliance method of accomplishing this is explained below (see Fig. 2).

1. A series of identical specimens is obtained with slightly differing crack lengths a_1, a_2, a_3.

2. A load (P) vs deflection (δ) trace is obtained for each specimen.

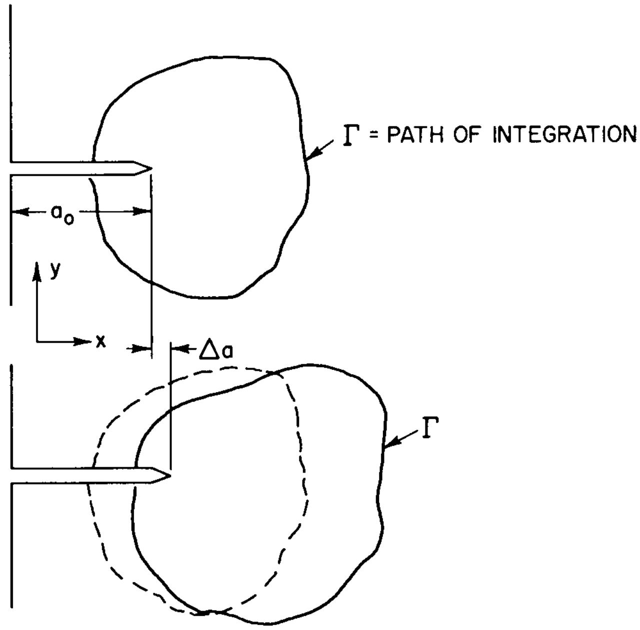
3. The area under the P vs δ trace for each specimen up to an arbitrary deflection δ_1 is the PE for that specimen deflection.

4. Other values of PE (as in step 3) are determined for different arbitrary deflections δ_1, δ_2, δ_3, from the original P vs δ traces.

5. A plot is made of PE vs crack length with the deflection as the parameter.

6. Finally, the slopes of the curves in step 5 at a given crack length are plotted vs deflection. Note that the slope is the negative of J (Eq. (1)).

7. The plot evolved in step 6 is used to determine the critical J for a new specimen, provided one knows the values of δ and a at instability.



$$J \equiv \oint_{\Gamma} \left[W\,dy - \vec{T} \cdot \frac{d\vec{u}}{dx}\,ds \right]$$

$$J = -\frac{d(PE)}{da}$$

Fig. 1 — The J integral, defined by the above equation, may be interpreted as the potential energy PE difference between two identical bodies having incrementally differing crack lengths.

J—INTEGRAL ESTIMATION PROCEDURES

The compliance method of evaluating J requires several specimens. In the case of irradiated material, which is usually in short supply, it is advantageous to employ one of several estimation techniques that require only one specimen. In this respect three estimation techniques, described below, have been used to evaluate 304 SS material.

Estimation Method 1

The first method was devised by Bucci and coworkers (4) and consists of estimating as opposed to directly measuring the load vs deflection curves for a series of identical specimens having different crack lengths. This estimation is based on the results of only one specimen test. Thereafter the procedure follows the outline for the compliance calibration method illustrated in Fig. 2 using only one specimen. Bucci and Coauthors have successfully predicted the same curves of J vs specimen deflection that were obtained by Begley and Landes (5) with the compliance procedure requiring several specimens.

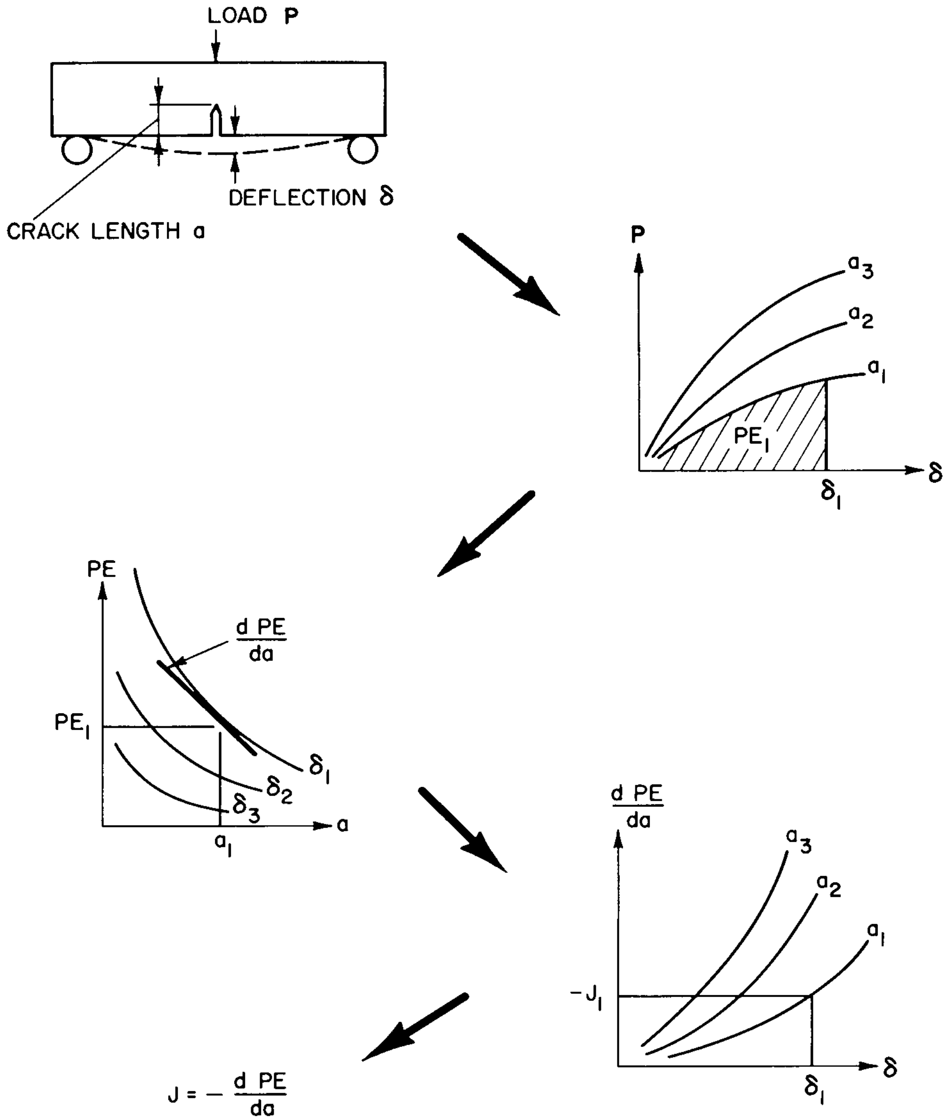


Fig. 2 — The J integral is readily evaluated experimentally from the load vs deflection curves (top right) of specimens having slightly differing notch lengths. Plotting the potential energy from these curves and differentiating graphically (left center) yields the compliance calibration curves (lower right). The value of J at the instant of crack instability is determined from these compliance curves using the critical values of deflection and crack length.

The estimation procedure for evolving the specimen load (P) vs midspan deflection (δ) curve for a three-point bend specimen is illustrated in Fig. 3. For linear elastic materials a family of straight lines through the origin is obtained for different crack lengths, a and $(a + da)$. For a rigid plastic material, the P vs δ curves are horizontal at a value of plastic limit load P_L for the given crack length specimen.* The remainder of the P vs δ

*With an actual specimen, however, fracture or slow tearing can prevent a specimen from reaching the limit load predicted on the basis of the original crack length. This is usually minimized by rounding the notch tip radius in a calibration test.

curve between the linear portion at the origin and the limit load is approximated by a plastic zone (r_y) correction to the linear elastic behavior. Details of the estimation procedure are described below.

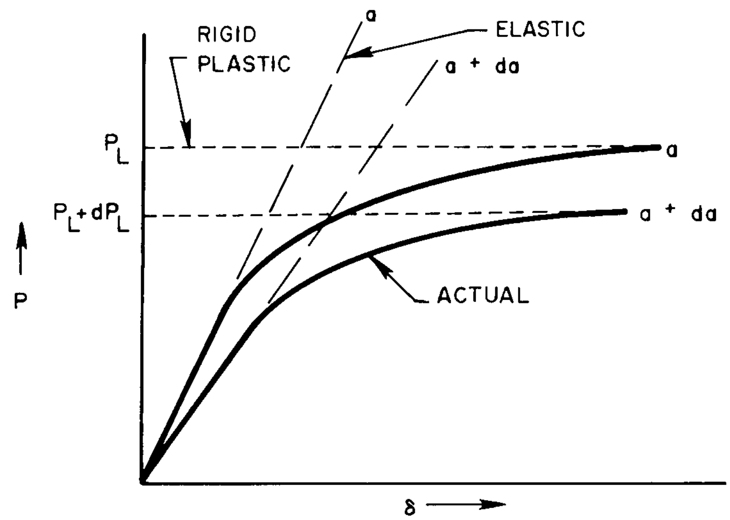


Fig. 3 — Estimation procedures for load vs deflection traces. The linear elastic and rigid plastic values (both dashed) are connected by r_y — corrected curves (solid).

Limit Load — The limit load for a three-point bend bar is taken as

$$P_L = 1.456\ \sigma_{ts} \frac{B}{S} (W - a)^2, \tag{3}$$

based on a solution for rigid plastic material developed by Green and Hundy (6). In Eq. (3) σ_{ts} is the uniaxial tensile strength, and B, S, W, and a are the thickness, span, width, and crack length, respectively. In theory the limit load could be calculated from Eq. (3) without a specimen test. However, some uncertainty exists in the degree of work hardening which should be factored into the equation through the choices of σ_{ts} and the 1.456 factor. Consequently, the measured limit load from an actual specimen that exhibits no slow tearing prior to limit load provides a better means of determining the constant in Eq. (3).

Linear Elastic Portion — The linear elastic portion of the P vs δ curves may be calculated from the following expression derived by Bucci and Coauthors (4):

$$\frac{EB\delta}{P} = 0.24\left(\frac{S}{W}\right)^3 \left[1.04 + 3.28\ (1+\upsilon)\left(\frac{W}{S}\right)^2\right] + 2c\left(\frac{S}{W}\right)^2\left(\frac{a}{W}\right)H\left(\frac{a}{W}\right), \tag{4}$$

where E and υ are Young's modulus and Poisson's ratio, respectively, and $c = 1$ for plane stress and $(1-\upsilon^2)$ for plane strain. The function $H(a/W)$ is given as

$$H\left(\frac{a}{W}\right) = 4.21\left(\frac{a}{W}\right) - 8.89\left(\frac{a}{W}\right)^2 + 36.9\left(\frac{a}{W}\right)^3 - 83.6\left(\frac{a}{W}\right)^4 + 174.3\left(\frac{a}{W}\right)^5$$
$$-284.8\left(\frac{a}{W}\right)^6 + 387.6\left(\frac{a}{W}\right)^7 - 322.8\left(\frac{a}{W}\right)^8 + 149.8\left(\frac{a}{W}\right)^9. \quad (5)$$

Plastic Zone Corrected Curve — The plastic zone correction yields an estimated P vs δ curve in the regime between the linear elastic portion (Eq. (4)) and the limit load portion (Eq. (3)). An effective a/W is defined as

$$\left(\frac{a}{W}\right)_{eff} = \left(\frac{a_0}{W}\right) + \frac{1}{\gamma\pi W}\left(\frac{K}{\sigma_{ys}}\right)^2, \quad (6)$$

where

a_0 is the original crack length and the second term is the conventional expression for plastic zone r_y

σ_{ys} is the uniaxial yield stress

$\gamma = 2$ for plane stress and 6 for plane strain. For a three-point bend bar an equation for K has been derived by Brown and Srawley (7) as

$$K = \frac{PS}{BW^{3/2}} F\left(\frac{a}{W}\right), \quad (7)$$

where*

$$F\left(\frac{a}{W}\right) = 2.9\left(\frac{a}{W}\right)^{1/2} - 4.6\left(\frac{a}{W}\right)^{3/2} + 21.8\left(\frac{a}{W}\right)^{5/2} - 37.6\left(\frac{a}{W}\right)^{7/2} + 38.7\left(\frac{a}{W}\right)^{9/2}. \quad (8)$$

Choosing a value of P, an $(a/W)_{eff}$ can be calculated from Eq. (6) via Eq. (7). Combining $(a/W)_{eff}$ with Eq. (4) gives a new value of $EB\delta/P$ which has now been plastically adjusted.

Formulation of J vs δ Curve — The final curves of J vs δ are obtained as follows:

1. Plot the linear elastic regime using the expression

$$J = \frac{\left[F\left(\frac{a}{W}\right)\right]^2}{W^3 E'}\left(\frac{S}{B}\right)^2$$
$$\times\ \delta^2 \frac{1}{\left\{\frac{0.24}{BE}\left(\frac{S}{W}\right)^3\left[1.04 + 3.28\,(1+\upsilon)\left(\frac{W}{S}\right)^2\right] + \frac{2}{BE'}\left(\frac{S}{W}\right)^2 \frac{a}{W} H\left(\frac{a}{W}\right)\right\}^2}, \quad (9)$$

*The coefficients in Eq. (8) apply only for $S/W = 4.0$. However, a negligible error is introduced when applying this expression for $S/W = 4.6$ which is the value used in the experimental program to be described.

where $E' = E$ for plane stress and $E/(1 - \nu^2)$ for plane strain. Equation (9) is a combination of Eqs. (4), (7), (8), and the fact that $J = \mathcal{G} = K^2/E'$ for linear elastic material. (Note that Eq. (9) is a parabolic function of δ.)

2. Determine the r_y — corrected portion of the J vs δ curves starting with the estimated P vs δ curves and then proceeding with the outline of Fig. 2.

3. Derive the rigid plastic portion of the J vs δ curves, noting that the potential energy per unit thickness PE is simply $P_L \delta / B$. Using this value of PE in Eq. (1) and expressing P_L with Eq. (3) yields

$$J = 2.912\, \sigma_{ts} \frac{\delta}{S} (W - a). \tag{10}$$

Observe that Eq. (10) for the rigid plastic material passes through the origin of the J vs δ plot. The actual J vs δ curves for the rigid plastic regime are approximated by maintaining the slope of Eq. (10) and offsetting this linear portion so that it is tangent to the r_y — corrected curves in step 2.*

Estimation Method 2

This method and Method 3 are based on work by Rice and others (8) and apply to a deeply notched beam in pure bending. Method 2 is based on an expression of K for a beam in bending as derived by Wilson (9):

$$K = \frac{4M}{(W - a)^{3/2}\, B}, \tag{11}$$

where M is the bending moment.

Incorporating Eq. (11) with Castigliano's theorem, a plastic zone correction, and a theorem of Rice's which relates J to the bending moment, it is possible to derive the following expression for J which is valid for loads less than the plastic limit load:

$$J = \frac{P\delta}{B(W - a)} \left[1 - \alpha D^2 \left(\frac{P}{P_L} \right)^2 \right], \tag{12}$$

where P and δ are the respective load and deflection at instability, P_L is the plastic limit load, and αD^2 is an experimentally derived constant whose value is approximately 0.35 (8). The advantages of this method are (a) J can be calculated in a simpler fashion than with Method 1 since the P vs δ curves for specimens of various a/W ratios are not required and (b) J can be determined from a single point on the P vs δ trace without computing the potential energy, i.e., area under this curve.

Estimation Method 3

This procedure (8) is based directly on Rice's theorem, which relates the bending moment and the angle of rotation of the notched beam using dimensional analysis and the equation for J:

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*See Fig. 5.

$$J = \int_{o}^{M} \left(\frac{\partial \theta_{tot}}{\partial a}\right)_{M} dM; \tag{13}$$

where M is the bending moment and θ_{tot} is the total angle of rotation. From this Rice has shown that

$$J = \frac{2A}{B(W - a)}, \tag{14}$$

where A is the area under a load vs deflection trace for a notched beam up to the point of crack instability.

For application of estimation methods 2 and 3 to bend tests, the deflection values used should eliminate the component of load point displacement which would be present for an unnotched beam of the same dimensions. This deflection may be obtained from the first term of Eq. (4) when the unnotched beam remains elastic under the imposed load. For cases of deeply notched beams, the unnotched deflection is usually small compared to the total deflection. Hence, the raw P vs δ curve may be used without a large error. The latter course was adopted in the experimental evaluation of J to be described.

EXPERIMENTAL DETERMINATION OF J FOR 304 SS

Materials and Test Procedures

To evaluate J for LMFBR structural materials, a program has been undertaken that focuses on the use of three-point bend specimens. In the case of 304 SS, these specimens were cut from a 1-in. plate that had received 25 percent CW. (See Table 1 for (1) mechanical properties and (2) chemistry.) Most specimens were fatigue cracked at a ΔK level of 20 ksi$\sqrt{\text{in.}}$ using zero-tension-zero loading. A portion of these was then solution annealed at 1900°F (1038°C) for 30 min. The material in both conditions was irradiated in the Advanced Test Reactor at a temperature of 525-545°F (274-285°C) and received a fluence of 5.27×10^{20} $n/cm^2 > 1$ MeV (fission) or 1.08×10^{21} $n/cm^2 > 0.1$ MeV (calculated).* Subsequent testing was performed at 550°F (288°C). Compression plugs 0.197 in. square by 0.433 in. long for flow properties were irradiated with the specimens. The evaluation procedure for the flow properties is given in Ref. 10.

The specimens were of Charpy-V dimensions but one-half Charpy-V thickness (width, W = 0.394 in., thickness, B = 0.197 in., and length, L = 2.165 in.). Testing was performed "statically" in three-point bend on a Krafft Dynamic Universal Loader using a span S of 1.81 in. The notch consisted of a 1/32-in. slot terminating in a 30-degree included angle; this was followed by a fatigue crack of approximately 0.060 in. to give an a/W ratio of 0.3, where a is the total notch length. The specimens were heated using an induction coil. The midspan deflection was measured using strain-gauged beams that followed the motion of the plunger which contacted the specimen at midspan. Load vs deflection, P vs δ, was recorded on an X-Y plotter.

*This irradiation exposure represents an initial J-integral investigation of stainless steels. The relatively low fluence and low irradiation temperature encompass the lower bound of conditions expected in an LMFBR, while at the same time they represent the conditions normally expected in a water reactor.

Table 1
Chemical Composition and Mechanical Properties

Type of Material	Composition, Weight Percent									
	C	Mn	P	S	Si	Ni	Cr	Mo	Co	Ta
304 SS Plate	0.050	1.48	0.028	0.016	0.53	9.53	18.57	0.7	-	-
308 S/A Weld	0.030	1.60	0.021	0.010	0.68	9.7	21.0	0.2	-	-

Mechanical Properties

	Unirrad. Annealed			Irrad. Annealed	Unirrad. Cold Worked			Irrad. Cold Worked		
304 SS Plate										
Temp. °F	RT	550	1000	550*	RT	550	1100	RT	550	1100
YS (ksi)	30	16.8	14	65	109	88.5	68.8	126.2	116.1	82.3
TS (ksi)	81.8	54.4	48	83	117	93.9	68.8	132.2	116.1	84.1
308 S/A Weld (Unirrad.)										
Temp. °F	RT		1000							
YS (ksi)	66		44							
TS (ksi)	107		60							

*Estimated

Results for Annealed 304 SS Plate

The P vs δ record for an irradiated 304 SS specimen in the annealed condition is shown in Fig. 4. Also presented are (a) the predicted P vs δ curves using plane strain and plane stress r_y corrections according to Method 1, and (b) the predicted limit load as obtained from Eq. (3). This specimen exhibited stable tearing throughout. Judging from the divergence of the predicted and actual curves and the failure to reach the predicted limit load, this stable tearing commenced before maximum load was attained in the specimen; however, techniques for specific observation of this slow growth have not yet been implemented. An approximation of the amount of slow growth before maximum load P_{max} can be made by solving Eq. (3) using the measured initial crack length (0.133 in.), the actual limit load, and the known σ_{ts}. This procedure suggests a slow growth of 0.025 in. at P_{max}. A duplicate specimen having a somewhat longer fatigue crack indicated a slow growth of 0.020 in. The applicability of Eq. (3) to stainless steel, however, must be confirmed, specifically the use of σ_{ts} as opposed to σ_{ys} and the value of the constant.

Figure 5 illustrates the curves of J vs δ that were estimated for plane strain conditions according to Method 1. (A similar set of curves for a plane stress r_y correction is presented in Fig. 6.) These curves were calculated for a/W ratios of 0.3, 0.4, and 0.5.

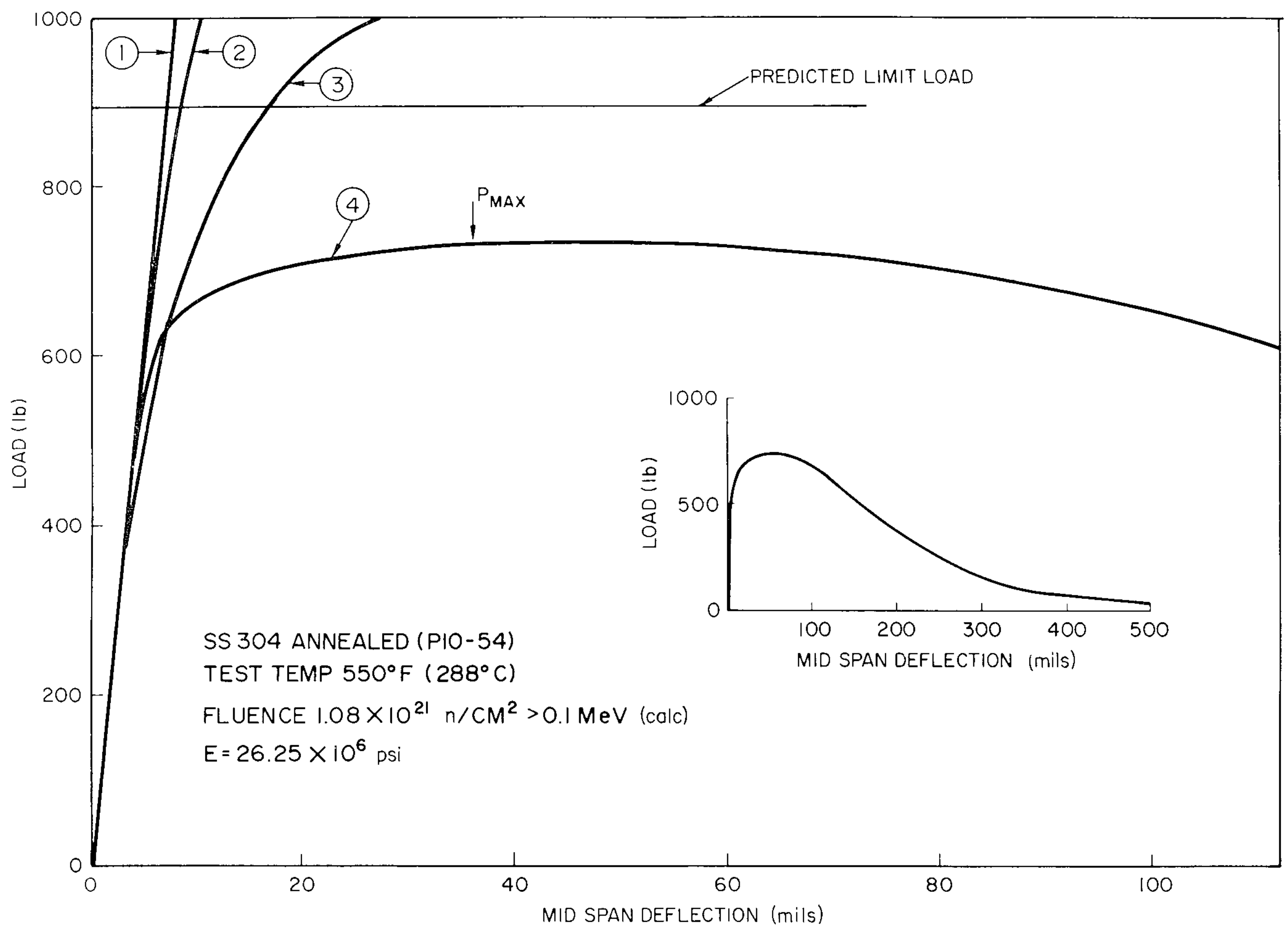

Fig. 4 — Comparison of actual and predicted load-displacement records for an anneal 304 SS specimen irradiated at 535°F (279°C). The curves are denoted as follows: 1 = linear elastic (plane strain and plane stress) from Eq. 4; 2 = linear elastic with plane strain r_y correction; 3 = linear elastic with plane stress r_y correction; 4 = actual record. The complete load vs deflection record is shown in the insert.

The actual specimen had an initial a/W of 0.338 as assessed from the fatigue crack length of the broken specimen. If 0.025 in. of slow growth occurred up to P_{max}, then the value of a/W at this load would have been 0.401.

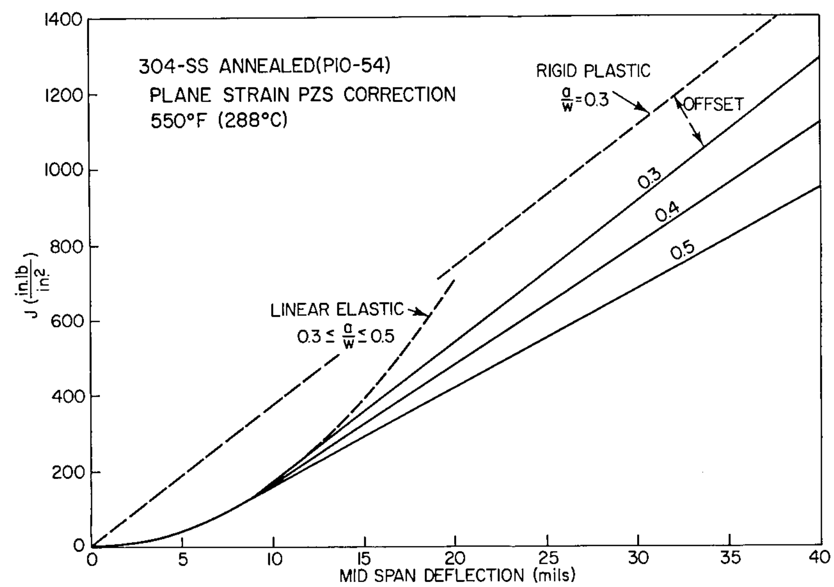

Fig. 5 — Calculated curves of J vs deflection for an annealed 304 SS specimen irradiated at 535°F (279°C) and tested at 550°F (288°C). A plane strain r_y correction was used.

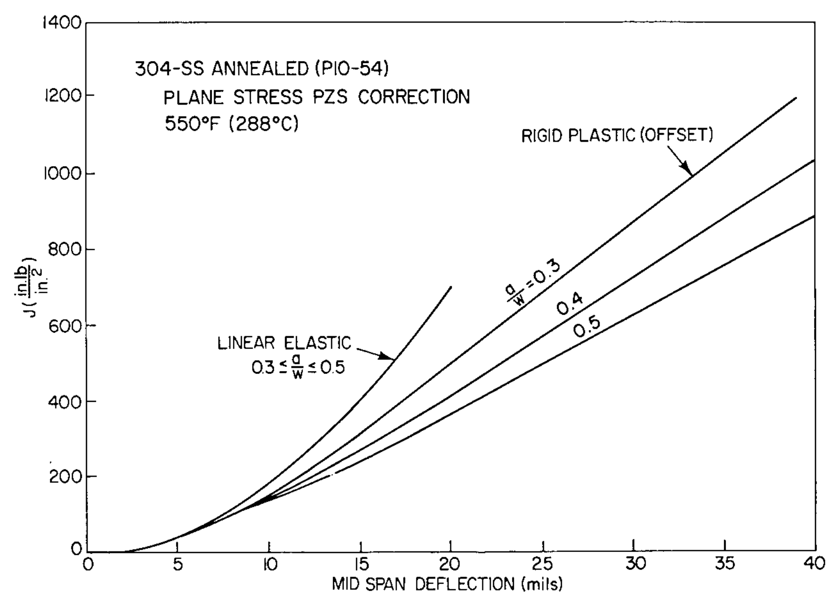

Fig. 6 — Calculated curves of J vs deflection using a plane stress r_y correction. The specimen is the same one defined in Fig. 5.

Since it was not possible to ascertain the load P at which slow growth began, this load was approximated as the value where the actual curve in Fig. 4 crossed the plane-stress-corrected curve, 0.007 in. This estimation results in a P/P_{max} ratio of 0.85 and is in agreement with visual observation of slow growth at a similar value of P/P_{max} on unirradiated specimens having machined notches (described later). Using the value of 0.007-in. deflection in Figs. 5 and 6 gives J values of 82 and 75 in.lb/in.^{2} for plane strain and plane stress conditions, respectively. It is not known which condition actually prevailed in the specimen, and indeed this condition may change throughout the test. The test record in Fig. 4 follows the plane strain curve, No. 2, more closely than the plane stress curve, No. 3. Note that a plane strain criterion suggested by Begley and Landes (5), $B >$ 50 J/σ_{ys}, would require a $B \geqslant 0.063$ in. as contrasted to the actual value of 0.197 in.

It is important to note the sensitivity of the J value to the specimen deflection. If, for example, J were estimated from Fig. 4 at the deflection of 0.035 in. corresponding to maximum load, a value of 960 and 880 in.lb/in.^{2} would be obtained from Figs. 5 and 6 for plane strain and plane stress, respectively. This assumes an $a/W = 0.401$ after 0.025 in. slow growth. For a duplicate specimen, a smaller J value of 600-530 in.lb/in.^{2} was determined at maximum load using a deflection of 0.023 in. This lower value of J is apparently the result of specimen material variation and the sensitivity of J to the value of deflection used in the evaluation. It is therefore readily apparent that any amount of slow stable growth that is permitted in J evaluations will have a significant bearing on the final calculated value of J.

A comparison of the three estimation procedures for calculating J is presented in Table 2. Good agreement is shown between Methods 1 and 3 at maximum load, with Method 2 giving somewhat lower values for this specimen.

Results for Cold-Worked Material

The P vs δ record for an irradiated, 25-percent cold-worked 304 SS specimen is illustrated in Fig. 7. The specimen exhibited a distinctly different behavior from the annealed material described above. The P vs δ record was fairly linear up to the maximum load with an indication of unstable growth popin near the maximum load. Linearity, however, was insufficient to determine a valid K_{Ic}. After maximum load the record showed sharp drops in load with little change in deflection, also indicative of unstable crack growth. It should also be noted that the ratio of yield to tensile strength is unity for this material in the irradiated condition as compared to approximately 0.4 for the annealed material (Table 1). The work-hardening coefficient n bears out these trends; i.e., n is small for CW material, typically 0.03 to 0.06, values values of n from 0.3 to 0.6 for annealed, unirradiated material. Which is to say, n for the annealed material is an order of magnitude larger than for the CW material (10).

A set of estimated J curves, similar to those in Figs. 5 and 6, was used to determine J for this specimen. Choosing a deflection of 0.011 in., corresponding to the distinct deviation from the calculated plane stress curve, the J values are 215 and 190 in.lb/in.^{2} for plane strain and plane stress, respectively. At a deflection of 0.014 in. corresponding to P_{max}, the J values are 340 to 310 in.lb/in.^{2} for plane strain and plane stress. The slow growth is estimated to be 0.030 in. for this specimen at P_{max}. As with the annealed specimens, this growth was determined from Eq. (3), assuming the deviation of P_{max} from the predicted limit load to be caused by slow tearing or popin for this specimen. A

Table 2
Summary of *J*-Integral Values for SS Plate and Weld Metal

Code	Spec. No.	Material*	Notch† Prep.	Test Temp. (°F)	Method 3		Method 2		Method 1	
					Crack Initiation (in.lb/in.^{2})	Max. Load	Crack Initiation	Max. Load	Crack‡ Initiation	Max. Load‡
P-10-54	1	I, A, WR	F	550	105	894	100	618	75-82	880-960
P-10-49	2	I, A, WR	F	550	-	548	-	426	-	530-600
P-10-33	3	I, CW, WR	F	550	247	366	265	341	190-215	310-340
P-10-36	4	I, CW, WR	F	550	177	222	216	250	160	215
P-10-45	5	U, CW, WR	F	550	-	325	-	330		
P-10-19	6	U, A, WR	F	550	-	2412	-	1760		
P25-52	7	U, A, WR	P	RT	1682	7172				
P25-14	8	U, A, RW	P	RT	2085	9446				
P25-15	9	U, A, RW	M	RT	2287	8055				
P25-55	10	U, A, WR	P	1000	-	1765				
P25-7	11	U, A, RW	P	1000	-	2618				
P25-8	12	U, A, RW	M	1000	-	3347				
P26-7	13	U, Weld	P	RT	660	974 est.				
P26-5	14	U, Weld	P	1000	-	1045				

*304 SS or 308 SS weld
I — irradiated to fluence of 1.08×10^{21} n/cm^2 > 0.1 MeV
U — unirradiated
A — annealed
CW — cold worked
RW and WR refer to specimen orientation

†F — fatigued
M — machined (60° tip angle)
P — machined (knife-pressed tip)

‡Range of values for plane stress or plane strain calculations.

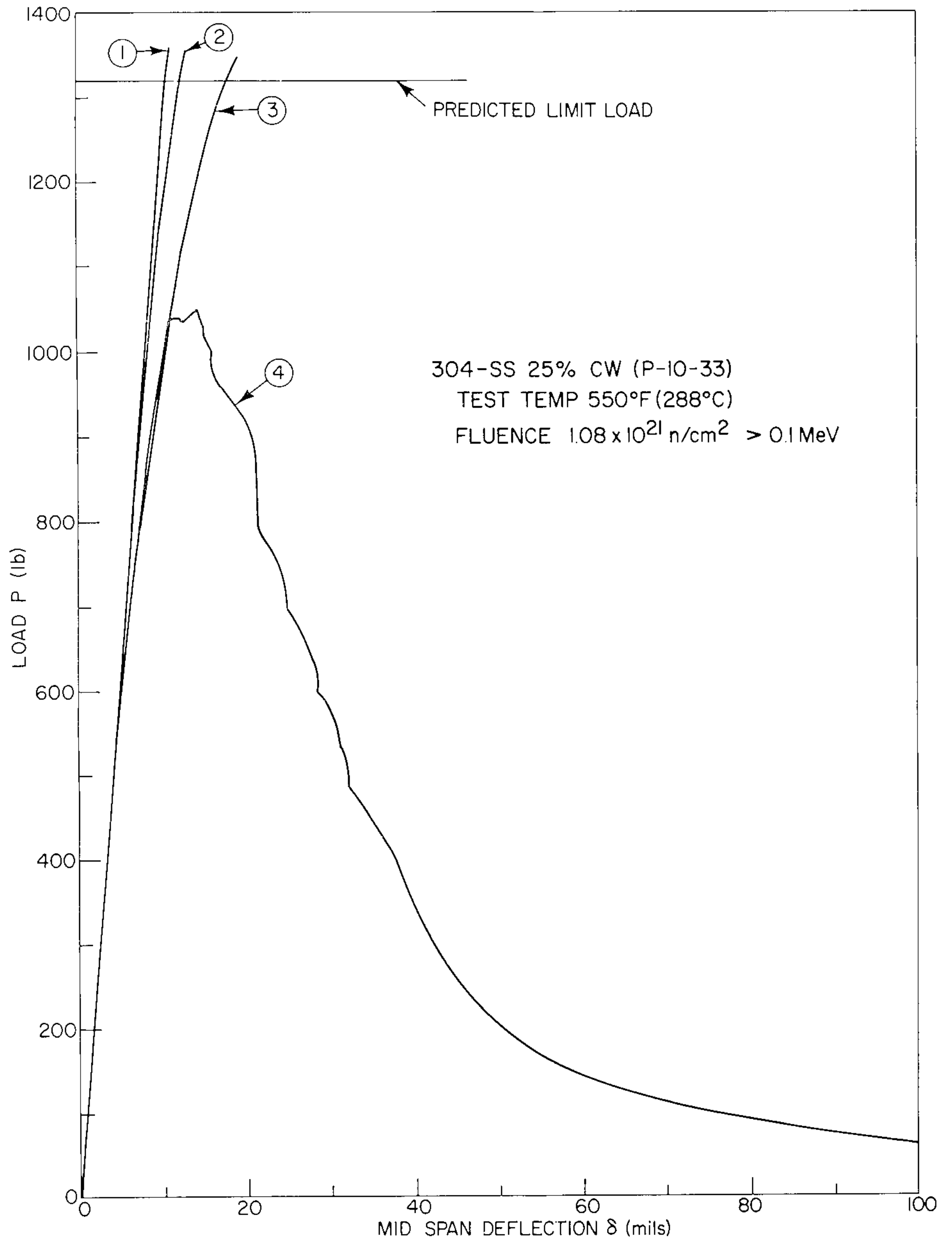

Fig. 7 — Comparison of actual and predicted load vs deflection records for a 304 SS specimen irradiated at 535°F (279°C). The numbers on the curves are defined in Fig. 5.

comparison of the estimation techniques for J (Table 2) indicates essential agreement among the three methods.

If the J value of approximately 200 in.lb/in.^{2} as determined above is in fact a property of the material, then one is able to predict a K_{Ic} value for a sufficiently thick specimen. For this case, $J = \mathcal{G} = (1 - \nu^2)(K^2/E)$ and $K = 76$ ksi$\sqrt{\text{in.}}$ This value of K_{Ic} can be measured with a specimen of 1-in. thickness, according to ASTM Committee E-24 recommended procedures. However, the 0.197-in. specimen thickness tested was sufficient for plane strain according to the Begley-Landes criterion.

Figure 8 presents a comparison of the *CW* and annealed materials in the irradiated condition. A physical interpretation of the difference in J values for these two specimens can be most easily obtained from Eq. (14) (Estimation Method 3). This equation gives J directly in terms of the area under the P vs δ trace up to the point of crack instability. If it is assumed that the maximum load and the corresponding specimen deflection define the point of crack initiation, then it is apparent that the area up to maximum load for the annealed material is several times larger than for the *CW* material; the J value for the annealed material is therefore proportionately greater than that for the *CW* steel. Qualitative assessments of the elastic plastic toughness of various materials can thus be directly obtained from the areas associated with the load vs deflection records.

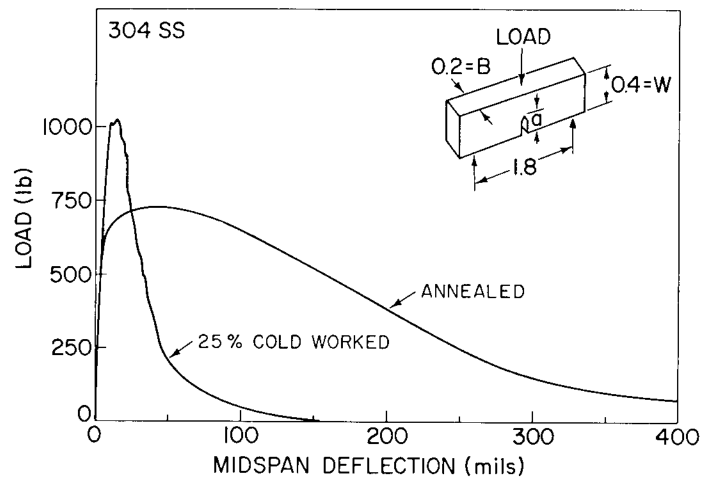

Fig. 8 — Comparison of load vs deflection records for SS specimens irradiated at 535°F (279°C) to a fluence of 1.08×10^{21} n/cm^2 > 0.1 MeV and tested at 550°F (288°C).

COMPARISON OF IRRADIATED AND UNIRRADIATED 304 SS

P vs δ traces for irradiated and unirradiated steels are presented in Figs. 9 and 10 for *CW* and annealed 304 SS, respectively. The results in Fig. 9 show the *CW* material to exhibit essentially the same behavior up to P_{max}. On the basis of P_{max} the J values by Method 3 are 325 and 366 in.lb/in.^{2} for the unirradiated and irradiated material, respectively (Table 2). This comparison suggests that the major degradation in J, in comparison to the annealed material for this fluence, is due to the process of *CW*; irradiation resulted in little additional toughness degradation.

The comparison of irradiated and unirradiated annealed 304 SS (Fig. 10) indicates that irradiation results in a reduction by a factor of four in the deflection to P_{max}. The consequent reduction in J at P_{max} by Method 1 is from 2412 to 894 in.lb/in.^{2} (Table 2). In certain structural applications the load-bearing capability must be based on P_{max}.

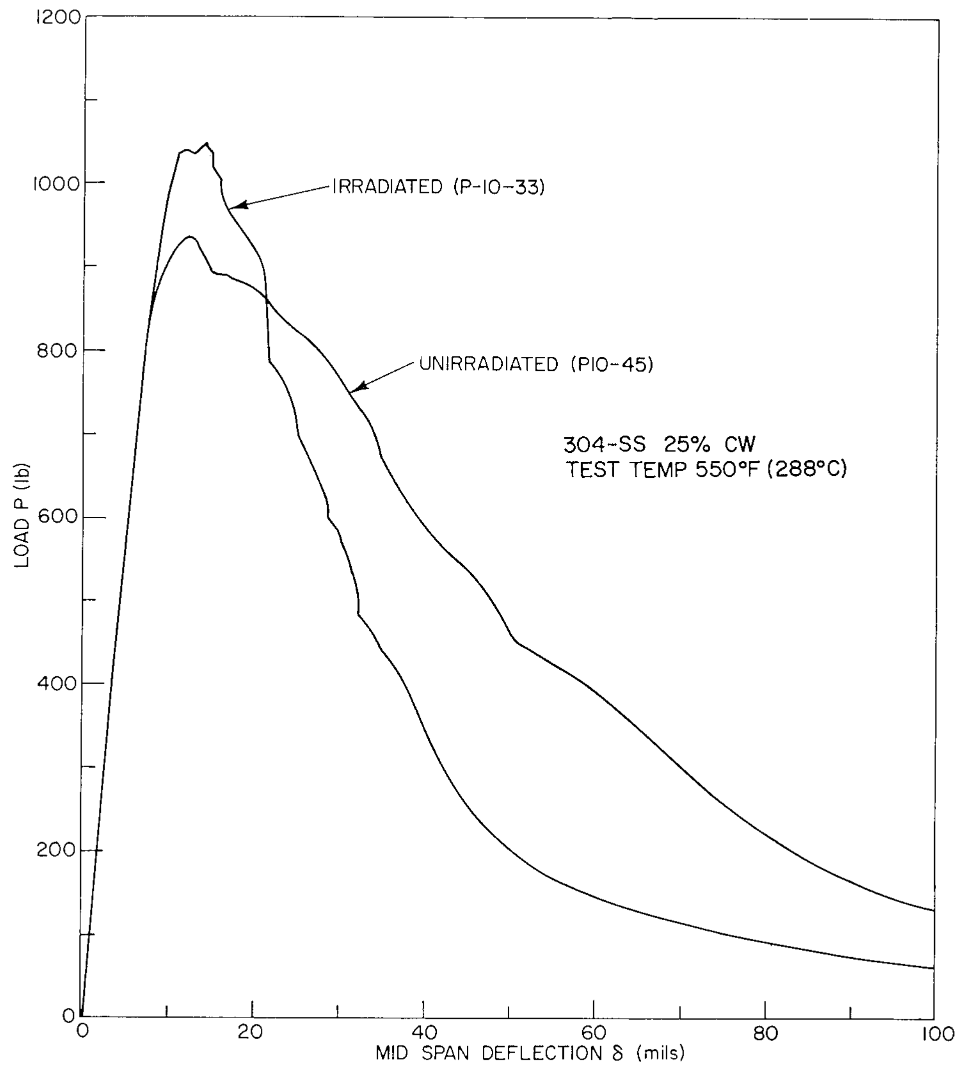

Fig. 9 — Comparison of load vs deflection traces for 25 percent cold-worked SS irradiated at 535°F (279°C) with the unirradiated material. The results show essentially the same behavior up to maximum load.

Structural members that cannot transfer load as a result of deformation (e.g., a pressure vessel) may be generally considered failed after reaching P_{max}. In this sense irradiation can have a severe detrimental effect on the strain tolerance of 304 SS in the annealed condition. As pointed out previously, however, the annealed material appears to be of significantly higher toughness than the *CW* 304 SS.

COMPARISON OF UNIRRADIATED 304 SS WELDMENTS

Figure 11 presents a room-temperature comparison of load vs deflection records for 308 SS submerged-arc weld deposit* and 304 SS plate. An elevated test temperature of

*See Table 1 for mechanical properties and chemistry.

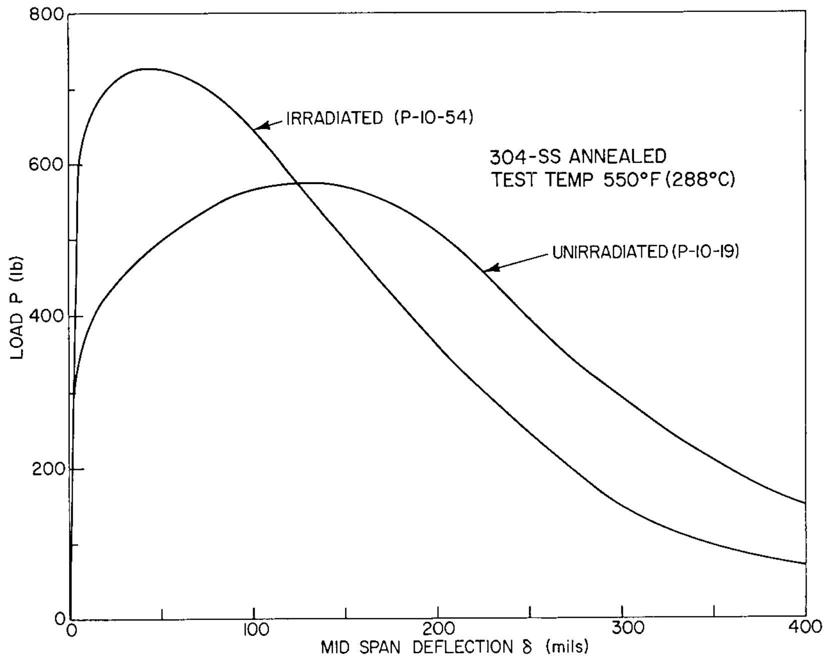

Fig. 10 — Comparison of load vs deflection traces for annealed SS irradiated at 535°F (279°C) with the unirradiated material. Irradiation produces a marked decrease in the specimen deflection and J value at maximum load.

1000°F (538°C) was chosen as representative of actual temperatures in an LMFBR. The specimens are of the same geometry previously described (S/W = 4.6, a/W = 0.3) but have machined notches. The notch tip was sharpened by pressing in a knife edge to a depth of approximately 0.003 in. These results illustrate that the weld metal exhibits a much smaller deflection and energy to maximum load than does the plate. Therefore, the values of J calculated on the basis of P_{max} would be significantly less for the weld metal than for the plate. (See Table 2, Specimen Nos. 7, 8, and 13.) Similar results were obtained for unirradiated specimens tested at 1000°F (538°C). (See Table 2, Specimen Nos. 10, 11, and 14.) For the weld metal specimen the elevated temperature did not appreciably change the energy to maximum load compared to the room-temperature test. However, the plate showed a sharp loss in energy to maximum load, and the J value calculated on this basis dropped to 25 percent of the room-temperature value (Table 2).

Also indicated in Fig. 11 are the loads at which visual observations were made of slow crack growth prior to maximum load. This growth commenced near the midthickness region of the crack and was not observed on the surfaces of the specimen until after P_{max} was achieved. The exact amounts of slow growth were not determined, however. The computed limit loads using σ_{ts} and Eq. (3) are likewise indicated on the figure. Equation (3) underestimates the observed limit loads by approximately 10 percent if no stable growth is assumed. Since some slow growth did actually take place, the measured limit load was less than that which would have been observed if there had been no slow growth; thus Eq. (3) would have underestimated the limit loads by an even larger percentage. This points out the need for further refinement in the applicability of Eq. (3) for stainless steels.

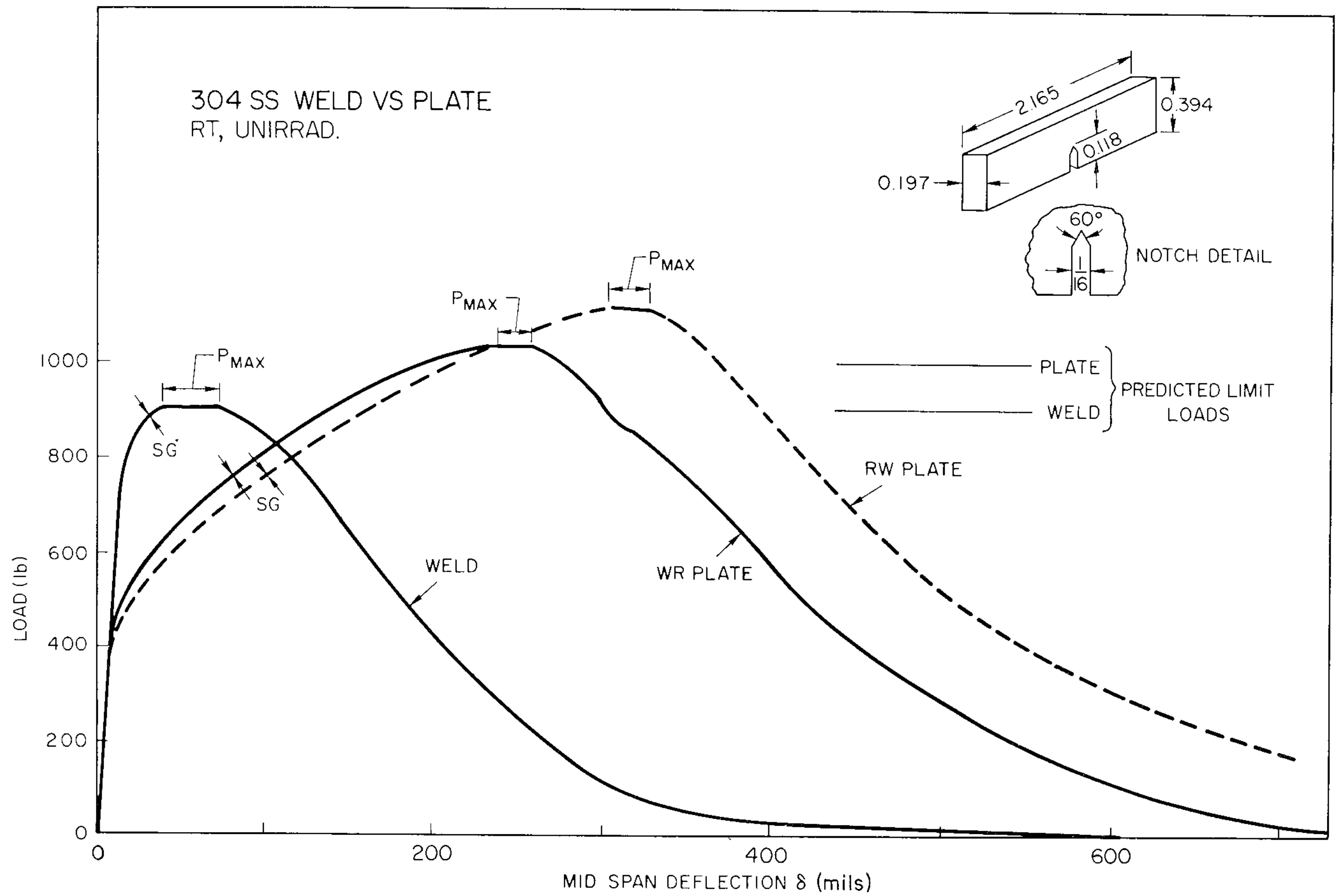

Fig. 11 — Comparison of load vs deflection records for 304 SS plate tested at room temperature in the strong (*RW*) and weak (*WR*) orientations and for 308 SS submerged-arc weld deposit.

Figure 11 shows the weld metal to absorb less total energy than the plate. This fact is consistent with the observation of Hawthorne (11) that the measured Charpy-V and dynamic tear energy absorption of the weld was significantly less (~ one-half) than that of the plate at temperatures up to 900°F (482°C). The results in Fig. 11 confirm this observation and show the ratio of total energy for the plate and weld to be 0.47 based on *RW* (longitudinal specimen orientation) plate energy and 0.37 based on *WR* (transverse specimen orientation) plate energy. However, the ratio of energy to maximum load for plate and weld defines an even greater degradation of weld performance as compared to the plate. Specifically, the ratio of energy to maximum load for the plate and weld is 0.2 based on *RW* plate energy and 0.15 based on *WR* plate energy. These results also suggest that energy to maximum load may be a more discriminating indicator of toughness than is total energy.

SUMMARY

Austenitic stainless steels that are used as structural materials in LMFBR reactor systems can become embrittled due to the service environment. This embrittlement may not be so severe as to result in linear elastic behavior that can be treated with classical fracture mechanics. Instead the toughness is expected to fall into the elastic plastic regime where a quantitative description of the critical flaw size vs stress level in terms of toughness is not generally available. The *J* integral is one method that is being considered to provide an approximate engineering solution over the near term in this area.

The advantage of the *J*-integral approach rests in the capability of bypassing the difficult notch-tip analysis which is required by other elastic plastic approaches. *J* can also be evaluated experimentally for different materials in a straightforward manner from the load vs deflection trace of a cracked specimen. As currently defined, however, the *J* integral is a mathematical approach and its usefulness as an engineering tool must be evaluated.

The practical application of *J* will be enhanced if it can be shown that this quantity is substantially identical for different flawed geometries. This fact will lend credibility to predictions of instability for structures of arbitrary geometry on the basis of a standard laboratory test. An optimistic assessment of this area has been noted through recent *J*-integral experiments by Landes and Begley (12) using A533-B and Ni-Cr-Mo-V rotor steels. These experiments indicated a constant value for J_c for specimens of different thickness and crack length, even though some of the specimens were of sufficient thickness to give valid K_{Ic} values whereas others were thin enough to result in fully plastic behavior.

Begley and Landes further suggested that the critical conditions for their materials in the elastic plastic regime may be controlled by an overriding singularity at the notch tip, as is the case of linear elastic materials. Verification of this fact would tend to minimize the effect of geometry and permit the emergence of *J* as a powerful technique with which to evolve fracture safety criteria. However, it would be premature to assume that the suggested geometry independence holds for all materials and all toughness levels. The required research will determine to what level of toughness the concepts of notch singularity and geometry independence apply. The final assessment of *J* must include structural element prototype tests that model the salient features of the structure.

It has been demonstrated here that the application of the J integral is complicated by the phenomenon of the slow stable crack exhibited by metals in the elastic plastic and fully plastic conditions. This stable process generally begins before maximum load is attained in a specimen. Slow stable growth is undesirable only from the aspect of modeling the material behavior; a limited degree of slow stable growth does not necessarily have a detrimental effect on structural integrity. The derivation of the J integral does not permit the material to be unloaded because of the irreversibility of plastic deformations discussed previously. Unloading will result when the stresses sustained by material close to the crack are relaxed if the crack front extends in a stable manner. The relevance of this behavior to the applicability of J as a failure criterion can only be assessed through further experimental investigations. From a practical viewpoint a standardized evaluation procedure must be developed that permits a certain amount of slow stable growth.

Finally, it should be recalled that J is defined mathematically only for two-dimensional behavior (i.e., plane strain or plane stress). The definition of maximum constraint corresponding to plane strain must be developed experimentally in the same sense that ASTM Committee E-24 has set the thickness limit as $B \geqslant 2.5\ (K_{Ic}/\sigma_{ys})^2$ for linear elastic behavior.

J values for irradiated 304 SS have been calculated using three different approximate techniques that each require only a single specimen test. Method 3 can be most easily understood in terms of specimen behavior. With this method J is simply proportional to the area under a load vs deflection trace for a bend specimen. Furthermore, the benefits of maximizing the deflection up to the point of crack initiation are easily visualized in terms of J. With this method the importance of choosing the proper specimen deflection with which to evaluate J also becomes apparent. For example, the J value computed at the deflection corresponding to maximum load can be quite different from J computed at a somewhat lower load and smaller deflection corresponding to crack initiation. Over the near term, it is expected that the resolution of this area of uncertainty will require the greatest portion of the J-integral research effort.

Comparison of J values for stainless steel in this report have been based, for the most part, on the condition corresponding to maximum load. With this criterion it appears that the toughness of 304 SS in the 25-percent CW condition is significantly below that of the material in the annealed condition, almost to the point of being linear elastic. Irradiation at the fluence level of $1 \times 10^{21}\ n/cm^2 > 0.1$ MeV changed the J values of the CW material very little from the unirradiated condition, whereas a fourfold reduction was noted for the irradiated, annealed material in comparison to the unirradiated, annealed material. The data also indicate that the unirradiated weld metal is of lower toughness than the annealed plate. Results from Charpy-V and dynamic tear testing of irradiated weld metal suggest that this trend of a lower J value for weld metal will continue to the irradiated condition. It is apparent that the weld metal could be the weak link in the fracture-safe assurance of an LMFBR system.

The conclusions described here pertain to an initial assessment of the J-integral approach to stainless steels. Additional work is underway to expand the range of fluences to higher levels and to consider the effects of different specimen geometry on J.

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